Tests and Modeling of Heat Generation and Heat Exchange in the Full Size Joint Sample

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We present the results of thermal-hydraulic tests and analysis performed on the Full Size Joint Sample (FSJS), specifically aimed at the validation of computational models for heat generation and heat exchange in a joint. These ingredients are essential for a proper predictive study of several items in the testing program of the ITER Toroidal Field Model Coil using, e.g., the MITHRANDIR or M&M codes. The tests also shed some light on the more general limitations of present heat transfer models in the cable bundle region.

1 INTRODUCTION

Joints are a crucial component – in a sense the “doors” – of super-conducting magnets for, e.g., nuclear fusion applications. Besides their obvious electromagnetic role, significant interest has arisen recently on their thermal-hydraulic behavior. A reason for this is given, e.g., by the measurement of current sharing temperature foreseen in the test program of the Model Coils (TFMC and CSMC) of the International Thermonuclear Experimental Reactor (ITER). There, external heaters will excite a perturbation, which, after propagating through the joint and along the conductor, should initiate a normal zone. These scenarios are indeed so delicate that a part of the experimental program on the FSJS − a set of praying-hands lap-type joints ~ 0.5 m long designed at CEA Cadarache, France − was explicitly devoted to thermal-hydraulic tests [1], which we performed at the Sultan facility of CRPP Villigen, Switzerland.

In the present paper, we first validate the heat generation model in the MITHRANDIR 2.1 code [2] against FSJS data. Subsequently, we address the problem of heat exchange between the two half joints. Steady state FSJS data, with external heating Q_{in} of *one* of the two legs, allow an approximate evaluation of the global heat transfer coefficient H, treating the joint as a standard heat exchanger. Here we study the performance of the Stainless-Steel (SS)-FSJS, which is representative of the TFMC inner joints. (The thermal-hydraulic tests of the TFMC-FSJS, which is representative of the TFMC *outer* joints, are not in our opinion so reliable [3] as to justify a detailed validation exercise). The global heat transfer coefficient between the two half joints, obtained from the experimental data, is then *critically* related to that which can be computed using the model typically implemented in the thermal-hydraulic codes for cable-in-conduit conductors, e.g., M&M [4]. A preliminary attempt to improve the model is then made, and an extrapolation to the TFMC inner joints is presented.

2 HEAT GENERATION IN THE JOINT

We consider here the thermal-hydraulics of Joule heat generation in the joint, due to finite current and electrical resistance. We use two different experimental runs of the Stainless Steel (SS)-FSJS, namely E12-16-022 (current $I(t)$ ramp-up from 0 to ~ 80 kA at ~ 100 A/s, short plateau of ~ 100 s, ramp-down to 0 kA at ~ 100 A/s, without external heating, nominal mass flow rate in each leg m ~ 5e-3 kg/s), and E12-16-027 (heat slug with symmetrical external heating ~ 50 W for 3 s, constant $I(t)$ ~ 80 kA, m ~ 2e-3 kg/s). Nominal conditions of operation guarantee that no heat transfer is present between the two half joints in both cases. Details on experimental configuration and runs can be found in [1].

The transient in the left leg (the only one where the mass flow rate was measured) is simulated with MITHRANDIR 2.1, assuming that the whole power $Q_J = RI^2$, goes uniformly into the strands in the joint (if Q_J is distributed between strands and jacket, the results do not change significantly). The joint resistance is

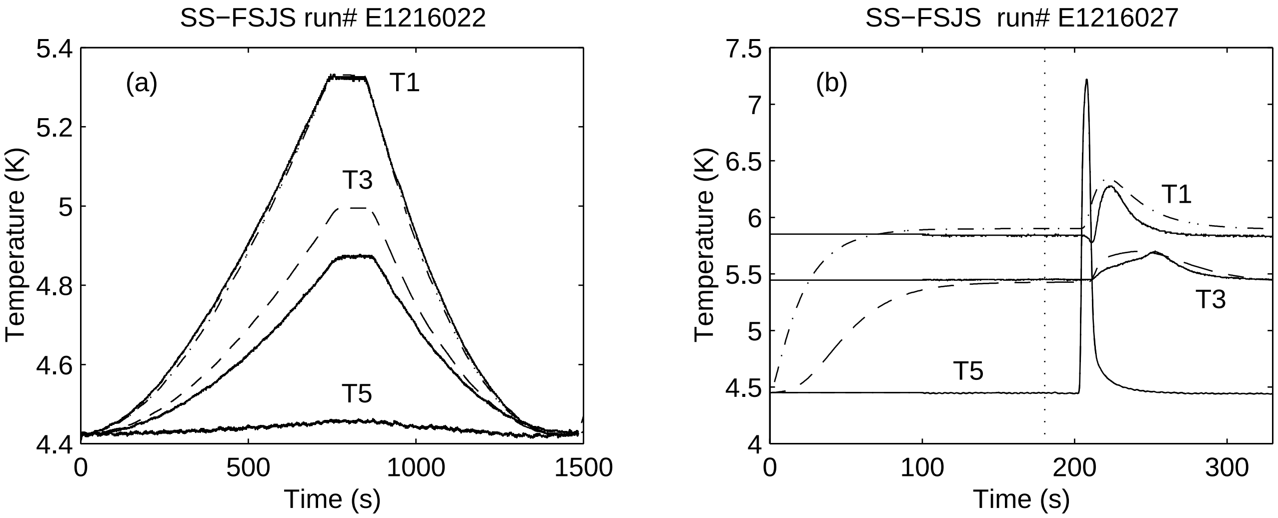

Figure 1 Comparison between experimental (solid lines) and computed (dash-dotted lines = T1, at joint outlet, dashed lines = T3, ~ half a meter further downstream in the conductor) temperatures, using T5(t) as inlet boundary condition, in two different cases: (a) current ramp–up and –down without external heating, (b) constant current with externally heated slug (symmetric in the two joint legs). In Fig.1b, the artificial initial transient on the left hand of the dotted line is needed to bring the simulated system to the proper initial condition with steady state Joule heat only, before the external heater is switched on at t ~ 200 s.

calculated as R = 1.e-9 * [0.54 + 0.1 * (B + 0.0164 * I), where B is the magnetic field in Tesla and I is in kA [5].

We see from Figs. 1a,b that the accuracy of the simulation is good in both cases, with ≤ 0.1 K difference between computed and measured temperatures. *The MITHRANDIR code is thus validated with respect to Joule heat generation in the joint.*

3 STEADY-STATE HEAT EXCHANGE BETWEEN THE TWO HALF JOINTS

For these tests, the standard methodology of heat exchanger analysis can be applied to a joint in order to get a first estimate of the global heat transfer coefficient H, together with the exchanged power Q_{exch} and the average (logarithmic) temperature difference ΔT [6]. This requires in the first place the knowledge of inlet and outlet helium temperatures, and of its total mass flow rate. While some of these quantities have been measured in the FSJS tests, *at least in principle* (see below), others have to be derived.

We make a number of simplifying assumptions: 1) The helium in the central channel is thermally isolated from the helium in the cable bundle, because of the thick (3e-3 m) wall, so that the actual heat exchange between the two half joints only involves the bundle mass flows; 2) The pressure drop *along the joint* is the same in the bundle and in the hole (although this is not exactly the case in more accurate models [2]); 3) The outlet temperatures are measured *at* the joint exit, i.e., before mixing between bundle and hole helium occurs (only roughly verified, see below). Notice finally that the assumption of constant properties in the joint, typical of standard heat exchanger treatments, is very rough for the helium specific heat C_p, in the range of temperatures (4.5÷7.5 K) and pressures (3.5e5÷9.5e5 Pa) of interest here.

RUN (xxx)	Q_{in}^L (W)	m_L (kg/s)	Re_L	Pr_L	Q_{in}^R (W)	m_R (kg/s)	Re_R	Pr_R	Q_{exch} (W)	ΔT (K)	**H (W/m^{2}K)**
004	0	1.9e-3	280	0.66	10	2.3e-3	407	0.73	3.88	0.91	**134**
005	0	1.8e-3	281	0.69	30	1.9e-3	410	1.07	10.3	2.26	**145**
006	0	1.9e-3	287	0.68	30	2.2e-3	463	0.99	8.76	2.00	**139**
007	0	2.9e-3	509	0.68	50	2.9e-3	627	0.98	14.8	1.72	**271**
008	0	4.7e-3	803	0.66	10	3.8e-3	654	0.68	3.73	0.25	**465**
009	0	4.8e-3	887	0.67	30	4.8e-3	948	0.73	11.8	0.57	**651**
010	0	5.0e-3	890	0.68	50	4.4e-3	929	0.82	19.2	0.85	**714**
011	10	4.8e-3	914	0.71	50	4.4e-3	917	0.84	14.4	0.62	**739**
012	50	5.2e-3	1112	0.84	10	5.9e-3	1151	0.71	17.9	0.50	**1124**
013	0	7.1e-3	1257	0.66	10	4.4e-3	782	0.67	3.54	0.19	**596**
014	0	7.0e-3	1264	0.66	30	6.2e-3	1204	0.71	11.5	0.42	**876**
015	0	6.9e-3	1274	0.67	50	6.5e-3	1328	0.76	20.0	0.64	**987**

Table 1 Results of the SS-FSJS analysis of steady state runs E12-16-xxx for different nominal input power Q_{in} and total mass flow rate m in each leg (L=left, R=right). Split of m between bundle and hole (typically 25÷30% in the bundle, 70÷75% in the hole), and Reynolds (Re) and Prandtl (Pr) numbers in the bundle helium has been computed.

3.1 Analysis of the SS-FSJS data

The SS-FSJS is a hydraulically co-current system (see Fig.2) where part of the jacket of the two CICC, put in contact through the joint, has been substituted by a copper sole [1]. A thin PbSn solder (δ_{PbSn} ~ 2e-4m) with low heat conductivity (κ_{PbSn} ~ 15÷20 W/mK) is interposed between the two copper soles. The heat transfer area for conduction through copper sole and solder is given by A_0 = 0.494(connected length)*0.064(Cu width) ~ 0.0316m^2, and $Q_{exch} = H\ A_0\ \Delta T$. The results of the analysis of all steady state runs we performed are summarized in Table 1. Notice that the mass flow rate was measured only in the right leg, therefore m_L in Table 1 is computed imposing the same enthalpy jump in both legs. Inlet temperatures have been measured on the feeding pipe before joint entrance (T5 sensors) while outlet temperatures have been measured in the conductor cable bundle, some tens of millimeters after the end of the joint (T1 sensors). Steady-state offsets have been corrected using as reference the Sultan sensors TI951, TI952 [1]. The efficiency $\varepsilon = Q_{exch} / Q_{in}$ of the heat exchanger, computed from Table 1, falls in the range 0.3÷0.4.

3.2 Comparison of experimental data with existing heat-exchange models

Assuming that the experimental H describes the heat exchange between bundle helium in the left and right half joints, we can in principle relate it to the theoretical heat transfer models presently used in existing multi-conductor codes, e.g., in the M&M code [4]. The global thermal resistance $1/(H^{theo}\ A_0)$ can be seen as the series of *four* resistances, see Fig.2: 1) helium to copper, 2) copper sole(s) of total thickness δ (conduction), 3) interface between copper soles (contact/conduction with heat transfer coefficient $h_{int} = \kappa_{PbSn}/\delta_{PbSn}$ in the SS-FSJS), 4) copper to helium. If the corresponding heat exchange areas are indicated by A_0 for mechanisms 2 and 3, and by A for mechanisms 1 and 4, we have:

$$1/(H^{theo}A_0) = 1/(h^*A)_L + \delta/(\kappa_{Cu}\ A_0) + 1/(h_{int}\ A_0) + 1/(h^*A)_R \quad (1)$$

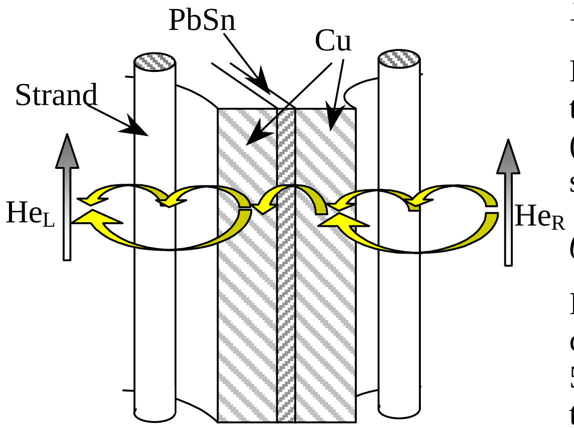

Figure 2 Schematic view of the heat flow in the SS-FSJS joint from the bundle helium (He) in the right (R) leg to the bundle helium in the left (L) leg, as modeled by the M&M code.

In each leg, the copper-to-helium resistance is due in turn to the parallel between the copper-to-helium resistance (convection) and the series of copper-to-strands (contact) and strands-to-helium (convection):

$$(h^* A) = h_{Cu\text{-}He}\, A_{Cu\text{-}He} + 1/[1/(h_{Cu\text{-}St}\, A_{Cu\text{-}St}) + 1/(h_{St\text{-}He}\, A_{St\text{-}He})] \quad (2)$$

In present-day thermal-hydraulic codes for CICC analysis the contact heat transfer coefficients have given values (e.g., $h_{Cu\text{-}St}$ = 500 W/m^{2}K) while the convective heat transfer coefficients are typically obtained from a correlation for the Nusselt number $Nu \equiv h\ D\ /\ \kappa_{He}$ (where D~0.47e-3 m is the hydraulic diameter), of the form $Nu = \alpha\ Re^{\ x}\ Pr^{\ y}$ (e.g., the truncated Dittus-Boelter correlation, $Nu = 0.023\ Re^{0.8}\ Pr^{0.4} \geq 8.235$), where the Re and Pr numbers are computed in the bundle region. Concerning the respective areas, $A_{St\text{-}He}$ is given as the wetted perimeter of the strands (since these are considered "very well" coupled by contact to the copper sole) times the connected length, while $A_{Cu\text{-}St}$ and $A_{Cu\text{-}He}$ are the width of the copper sole in contact with strands and helium, respectively, times the connected length.

Under the above assumptions, it turns out that H^{theo} ~ 400 W/m^{2}K ~ constant (being the Nu always truncated at Nu = 8.235 for the runs of Table 1) as opposed to an H varying between 100 and 1000 W/m^{2}K (see Table 1). Among the possible remedies to this discrepancy we have isolated two, which will be addressed in more detail below: 1) use/development of a different correlation for the convective heat transfer coefficients, 2) simple model for the assessment of the effective convective heat transfer area.

3.2.1 Towards an improved heat transfer model in the cable bundle region

Concerning the convective heat transfer coefficients, we do not consider the accuracy and setup of the FSJS tests as a good basis for the *development* of a new correlation, for which aim a dedicated test should be designed. On the other hand, the treatment of the cable bundle as a saturated porous medium [7] not in local thermal equilibrium opens up a completely different and general approach to the problem. Let us give a simple example: when, as in our case, the heat conductivity of the solid matrix (i.e., ~ 10^3 W/mK for the strands) is orders of magnitude larger than that of the fluid (i.e., ~ 10^{-2} W/mK for the helium), the inter-phase heat transfer coefficient in a packed bed of spheres can be correlated for $Re_p > 10^2$ (Reynolds number computed using the *particle* diameter, i.e., in our case the strand diameter d_P = 0.81e-3 m) can be correlated by $Nu \equiv h$

$d_P / \kappa_{He} = (0.255/\phi) Re_p^{2/3} Pr^{1/3}$, where ϕ is the porosity (i.e., the void fraction) [8]. (Notice that, in principle, a different correlation should be used for h_{Cu-He} [8].) If applied to our case, this gives h_{St-He} ~ 2000÷5000 W/m^{2}K for the range considered in Table 1, as opposed to h_{St-He} ~ 250 W/m^{2}K computed with truncated Dittus-Boelter, which applies strictly speaking only to turbulent flow in circular channels.

Concerning the effective convective heat transfer area, we have developed a simple 1-D model where successive layers of strands, partly in contact with each other as in the cable bundle, are interspersed with layers of flowing helium acting as an infinite heat capacity sink, and possibly, once in a while, with other, thinner solid layers simulating the wrappings. The decay length of the strand temperature from a surface heating source at given temperature (or given flux) can be easily computed. It turns out that after 4÷5 layers of strands the strand temperature has reached that of the cooling helium. (Of course, definition of a volume average over such a small region may be borderline for the porous medium approach). Considering now that there are about 40÷50 "layers" of strands in half a circumference of the cable bundle region, the effective convective heat transfer area results about an order of magnitude lower compared to that used in Section 3.2.

When the previous two ingredients are put together in the estimates (1)-(2), it turns out that H^{theo} ~ 300÷700 W/m^{2}K with some improvement in the agreement with H, with respect to the previous estimates.

3.3 Extrapolation to the inner joints of the Toroidal Field Model Coil

There are a number of caveats in this extrapolation: 1) The joints in the TFMC will be shaking-hands and the helium flow in the two connected conductors will be counter-current, both as opposed to here; 2) Nominal mass flow rates will be ~ 18e-3 kg/s/pancake, i.e., significantly beyond the Reynolds range tested in the FSJS; 3) Operating pressure will be at ~ 3.5e5 Pa instead of ~ 9.5e5 Pa as here. However, in the SS-FSJS the so-called $NTU \equiv HA/mC_p$ [6] grows linearly with m, so that the correspondingly higher NTU at 18e-3 kg/s could be extrapolated. Furthermore, if the joint structure and A_0 are the same, H for given mass flow rate should also be the same in the TFMC. But at 3.5e5 Pa C_p is on average higher, so that this would correspond to approximately the same NTU for TFMC and SS-FSJS. For comparable values of the capacity rate ratio $(mC_p)_L/(mC_p)_R$, the ε of the counter-current TFMC joint should then be somewhat larger (~ 0.4÷0.5) than that of the co-current SS-FSJS [6].

4 CONCLUSIONS AND PERSPECTIVE

The MITHRANDIR code has been validated against heat generation in the joint. Analysis of heat exchange data between the two half joints has shown that in the SS-FSJS the global heat transfer coefficient H results in the range 100÷1000 W/m^{2}K, for mass flow rates ~ 2e-3÷7e-3 kg/s, while ε ~ 0.3÷0.4. For the TFMC inner joints, ε ~ 0.4÷0.5 could be extrapolated. Unfortunately, the experimental values of H cannot be easily justified in terms of simplistic heat transfer models, which predict a constant value ~ 400 W/m^{2}K. More detailed treatments, e.g., based on porous medium theory, appear promising and will be further pursued in perspective. Dedicated experiments, specifically designed for elucidating heat transfer mechanisms in the cable bundle region in general, are also missing and needed.

ACKNOWLEDGMENTS

The European Fusion Development Agreement (EFDA) and the Italian Ministry for University and Scientific and Technological Research (MURST) have partially financially supported the present work.

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